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PHYSICS OF REACTOR SAFETY

Quarterly Report

April—June 1980

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PHYSICS OF REACTOR SAFETY

Quarterly Report
April-June 1980

ABSTRACT

This Quarterly progress report summarizes work done during the months of April-June 1980 in Argonne National Laboratory's Applied Physics and Components Technology Divisions for the Division of Reactor Safety Research of the U.S. Nuclear Regulatory Commission. The work in the Applied Physics Division includes reports on reactor safety modeling and assessment by members of the Reactor Safety Appraisals Section. Work on reactor core thermal-hydraulics is performed in ANL's Components Technology Division, emphasizing 3-dimensional code development for LMFBR accidents under natural convection conditions. An executive summary is provided including a statement of the findings and recommendations of the report.

FIN No.Title

A2015

Reactor Safety Modeling and Assessment

A2045

3-D Time-dependent Code Development

EXECUTIVE SUMMARY

BIFLO was extensively modified to permit the use of a much improved formulation of the coupling together of two-phase and single-phase regions. Programming of the initial BIFLO version has been completed to the extent that the code can now be used for benchmarking calculations and computational studies. A one-channel (i.e. one-dimensional) comparison was performed between BIFLO and a SAS calculation of a loss-of-flow transient in a CRBR type subassembly and good agreement was obtained between the two codes both before and after the onset of boiling.

The intercomparison of computer codes and experiment data at the the 19-pin level is in progress using the BIFLO, COMMIX-1A, and COBRA-IV-I computer codes and the experiments performed using Bundle-6A in the THORS Facility at Oak Ridge National Laboratory. Analyses are indicating that the mixing at steady-state in Bundle-6A is somewhat less than would be calculated using recommended values for mixing parameters in the several computer codes. Detailed data have been obtained for three transient THORS tests which proceeded into boiling, and the data are being analyzed.

The UK NEFIG fission gas release model has been incorporated into SAS/EPIC. Provision has been made for a user-specified fraction of fission gas to be released linearly with temperature between 1773 K and 2173 K, at which point the NEFIG calculation is started. Gas still remaining when the solidus temperature is reached is released linearly with melt fraction. A number of parameters in NEFIG may be input by the user.

Results are presented for our contribution to the WAC Group 0.10 \$/s TOP comparative exercise. The assumed clad failure criterion for irradiated pins was .50 melt fraction at 80% of core height. A considerable amount of molten fuel was ejected, resulting in a strong FCI with the 100 μ m radius fragmented fuel particles assumed. A power rise to 7 times normal occurred from positive sodium voiding feedback before the excursion was terminated by negative fuel motion feedback.

A report on the UK/NRC program to date was essentially completed. Planning of the next phase of the program is under way.

In the development work of the COMMIX codes, all efforts this quarter were devoted to the two-phase COMMIX-2 code. The following three problems were analyzed using the line-by-line solution procedure with SIMPLER formulation incorporated in COMMIX-2.

- (i) Two-Phase Flow in a Heated Duct: In this problem subcooled liquid (H_2O) enters a rectangular heated duct and begins to boil as it flows through it.
- (ii) NRC Standard Problem #1: This problem involves flash boiling due to sudden decrease in pressure.
- (iii) German Seven Pin Hexagonal Fuel Assembly: This analysis considered the flow rundown transient in a test section with seven electrically heated rods arranged in a hexagonal array.

The results of COMMIX-2, for all these three problems were compared with the experimental data and were found to be in good agreement. Simultaneously, efforts were also devoted to developing the Analytic Rebalance solution technique and to developing the thermal structure model for COMMIX-2.

In the area of BODYFIT code development, the generalized formula for the pressure correction, applicable to three-dimensional flows, was derived, formulated and implemented in the code. The coordinate generation code has been extended to generate coordinates for square geometry. The German seven pin sodium boiling experiment was analyzed with the BODYFIT code. The results, when compared, were found to be in satisfactory agreement with the measured data. Considerable amounts of effort were devoted to cleaning up the code and making it more user oriented.

I. REACTOR SAFETY MODELING AND ASSESSMENT

(A2015)

A. BIFLO Development (J. J. Sienicki)

Programming of an initial version of BIFLO has been completed to the extent that the code can be used in benchmarking calculations and computational studies. Although the BIFLO numerical framework (as it existed at the time) was sketched in the last Quarterly Progress Report,¹ the code has been significantly modified during the present reporting period. A brief discussion of the current BIFLO approach will be presented here together with a discussion of the results of a comparison between BIFLO and a SAS calculation. A detailed description of the code would be too long to be presented here and will be documented later.

BIFLO is an Eulerian code and models a pin bundle in terms of a few axial channels which are assumed to be interconnected so that sodium is free to flow between adjacent channels. Although it is currently assumed that each channel corresponds to one or more of the hexagonal coolant rings in a pin-bundle, this restriction could be readily removed to permit the treatment of a bundle subjected to a power skew. A staggered Eulerian numerical grid in which velocities and momenta are defined at cell edges and pressures, enthalpies, temperatures, densities, volume fractions etc., are defined at cell centers is assumed. Different numerical methodologies are employed within two-phase and single-phase regions of the Eulerian grid.

In the single-phase regions, use is made of a simplifying assumption that the local axial pressure drop is identical for all single phase channels. A radial momentum equation is not solved. Instead, the effects of transverse flow due to wire wraps is modeled with experimentally calibrated crossflow terms specifying mass exchange between channels. There is also a calculation of crossflow due to flow diversion which can be significant near two-phase/single-phase interfaces. The single-phase calculations within BIFLO are highly implicit and achieve very fast running times for the case of a few channels. The basis for the BIFLO approach for calculating single-phase thermal hydraulic conditions and its computational advantages have been discussed earlier^{2,1} and we will not recount them here.

In two-phase regions, an explicit formulation of the TWOPOOL strategy for calculating two-phase thermohydrodynamic conditions at saturation is currently employed. In this approach, it is assumed that the liquid sodium and sodium vapor remain in thermodynamic equilibrium at saturation. The pressure in a two-phase numerical cell used in determining thermohydrodynamic motions is assumed to be the saturation vapor pressure. The effects of crossflow due to wire wraps is currently modeled with a generalization of the wire wrap crossflow term assumed in the single-phase regions. Because radial temperature incoherence can give rise to large radial pressure gradients after saturation conditions are attained, there is also a calculation of pressure driven radial two-phase flow which is modeled using liquid and vapor radial momentum equations. Uncertainties here include the modeling of wire wrap induced crossflow and radial frictional effects within pin bundles under two-phase conditions. The computational advantages of the TWOPOOL/BIFLO saturation approaches have been discussed previously.^{1,3}

The two-phase axial and radial momentum equations are also assumed at two-phase/single-phase interfaces. During the past quarter, BIFLO was extensively reprogrammed to permit the coupling between the two types of regions to be formulated in this manner. The current BIFLO formulation has eliminated much of the difficulty in coupling together the two-phase and single-phase regions encountered with an earlier developmental version of the code.¹ The calculation of radial velocities accounting for the effects of pressure driven transverse flow at two-phase/single-phase interfaces represents a particularly significant improvement. This permits us to model situations in which pressurization in boiling central channels attempts to "squeeze out" the surrounding single-phase liquid bypass flow. This phenomenon could significantly influence void propagation during a boiling transient.

During the past quarter, a one-channel (i.e. one dimensional) comparison between BIFLO and a SAS calculation of a loss-of-flow transient in CRBR⁴ was carried out. Output records for the SAS run (referred to as Case 4 in Ref. 4) were furnished by C. H. Bowers (ANL/RAS). Although the SAS calculation considered ten channels, our comparison was limited to the behavior in only the highest power channel (Channel 1). Our major objective was to compare the times at which boiling inception occurs and the time intervals between the onset of boiling and inlet flow reversal calculated with the two codes.

To permit the axial power profile to be determined directly from the SAS input, the BIFLO numerical grid was defined so that each SAS heat transfer node within the active core and axial blankets corresponded to exactly one or more BIFLO nodes. The normalized total power as a function of time was obtained from the SAS output. The hydraulic modeling of the one-dimensional channel (i.e. the definition of friction factors, fluid inertial lengths, inlet and outlet loss coefficients etc.) was defined to be identical to that used in SAS. The SAS outlet plenum pressure and the "inlet pressure" which is defined here as the pressure that one would calculate to exist at the inlet to the subassembly were used as pressure boundary conditions for the BIFLO calculation. The outlet plenum pressure in the SAS calculation was held constant and this constant was therefore used in the BIFLO calculation. The inlet pressure as defined above was specified with the aid of an analytical fit obtained from results printed in the SAS output. Our overall characterization of the inlet pressure is judged to be a very good one in the sense that when BIFLO calculations are performed with boiling suppressed (so that single-phase thermal hydraulic conditions are maintained) over the time interval of interest (0 s - 20 s), the flowrate calculated by BIFLO agrees to within a few percent with the prescribed flow coastdown presented in Ref. 4. The sodium thermophysical properties used in BIFLO for the comparison are believed to be identical to those used in the SAS calculation.

SAS and BIFLO employ very different heat conduction models to calculate heat transfer between the sodium coolant and the fuel pins and hexcan. To perform a rigorous comparison between only the thermohydrodynamic portions of the two codes would require the use of the same heat transfer model. Because it would be too time consuming to carry out such a comparison (because of the modifications which would have to be made to either one or both of the codes), it was decided not to attempt a comparison more detailed than the one which we have performed. In an effort to make the BIFLO heat transfer calculation

similar to the one in SAS, thermophysical properties used in the BIFLO heat transfer model were defined on the basis of the SAS input and a constant fuel-clad gap conductance believed to be representative of that calculated within SAS was employed.

Excellent agreement between the two codes was obtained for the calculated time of boiling inception. BIFLO predicted the onset of boiling to occur at 11.35 s following the initiation of the flow coastdown while in the SAS calculation, boiling commenced at 11.46 s. Although we do not show plots of the flowrates and the coolant temperature and pressure profiles calculated with the two codes, excellent agreement was obtained for these variables for times between the initiation of the flow coastdown and the onset of boiling.

The agreement between the two codes obtained for the time interval between boiling inception and inlet flow reversal was, however, not as good. BIFLO predicted inlet flow reversal to occur 0.40 s after boiling inception while SAS calculated flow reversal 0.55 s following the onset of boiling. However, considering the numerous differences between the boiling models in the two codes, the results of this initial comparison were still encouraging.

The BIFLO calculations of this particular problem were found to be strongly influenced by Eulerian numerical affects. Fig. 1 shows the subassembly inlet and outlet flowrates calculated with BIFLO following the onset of boiling while Fig. 2 shows the corresponding locations of the upper and lower two-phase/single-phase interfaces (i.e. the boundaries of the two-phase boiling region). In Fig. 2, the vertical axis begins at the bottom of the lower axial blanket and continues to the top of the fission gas plenum. The calculation modeled this portion of the subassembly with forty-seven axial nodes. Boiling commenced at the top node within the active core. The flowrates exhibit a number of abrupt slope changes. Comparing Fig. 1 with Fig. 2, it is seen that some of these abrupt slope changes correspond to movements of two-phase/single-phase interfaces suggesting that Eulerian effects play a significant role in the determination of the flowrates. The calculations considered here employed a purely Eulerian methodology in which two-phase/single-phase interfaces must correspond exactly to numerical grid cell edges and two-phase velocities at those interfaces are calculated using a pressure gradient defined in terms of the saturation vapor pressure within the two-phase cell and the single-phase pressure in the adjacent single-phase cell. The two-phase pressure may be thought of as a pressure source applied to the column of single-phase liquid which is located either above or below. As long as a particular two-phase cell is adjacent to an interface, its saturation vapor pressure is applied to the neighboring single-phase liquid. However, when the interface is calculated to move, the saturation pressure within a different two-phase cell suddenly (over the course of one time step) becomes the effective pressure source which can result in the calculation of an abrupt change in the flowrate. The sensitive variation of the saturation vapor pressure with temperature enhances the changes in the effective two-phase pressure source accompanying the motion of the interface. Fig. 1 shows examples of situations where movement of the interface results in the replacement of a two-phase pressure which has been accelerating a fluid column by one which decelerates it. While real chugging phenomena are expected to occur and, indeed, are seen in sodium boiling experiments, some of the oscillations

observed in Fig. 1 are purely numerical in origin and would be expected to vary (in both magnitude and width) with the numerical grid cell size. The problem here is one of insufficient spatial detail; that is, the calculation

Fig. 1. Inlet and Outlet Flowrates Calculated with BIFLO.

assumes a grid which is too coarse. This is further indicated by the voiding behavior observed in our calculations. After a numerical grid cell goes into boiling, it is usually observed to void to a fairly high void fraction (e.g. $VR_v \geq 0.5$) before the neighboring single-phase cell reaches saturation conditions and can be considered for the single-phase to two-phase transition. Thus, the numerical grid cell size is too large to properly track a two-phase/single-phase interface. Ideally, the grid should be fine enough that the interface can move across several cells before any one of those cells undergoes significant voiding.

In an effort to assess the possible magnitude of Eulerian numerical effects on the BIFLO calculation without decreasing the numerical grid cell size, a BIFLO calculation was performed with the two-phase pressure used to

Fig. 2. Two-Phase/Single Phase Interphases Calculated with BIFLO.

drive the velocities at two-phase/single-phase interfaces replaced by an interpolated saturation vapor pressure. Consider the case where the upper interface is located at cell edge $j + 1/2$ so that cell j is two-phase and cell $j + 1$ is single-phase. Instead of using the saturation vapor pressure $P_{sat,j}$ in cell j to calculate the two-phase velocities at $j + 1/2$, we now use the interpolated saturation vapor pressure defined by

$$(1 - VF_{v,j}) P_{sat,j} + VF_{v,j} P_{sat,j+1} \quad (1)$$

where VF_v is the vapor volume fraction. Eq. (1) is expected to result in a less abrupt variation of the two-phase pressure source applied to the upper

liquid slug between $P_{sat,j}$ and $P_{sat,j+1}$ as cell j voids and the interface moves from $j + 1/2$ to $j + 3/2$. It is thus expected that the use of Eq. (1) together with a similar expression for the lower interface might result in a more realistic calculation. However, it should be noted that an interpolation in terms of the void fraction is far from ideal, because cell $j + 1$ can make the transition from single-phase to two-phase flow while the void fraction in cell j is still significantly below one.

SQUARE : INLET FLOWRATE

CIRCLE : OUTLET FLOWRATE

Fig. 3. Inlet and Outlet Flowrates Calculated with BIFLO.

Figs. 3-4 show the results of a calculation performed using the interpolation procedure defined by Eq. (1). Significant differences are observed between the results of the present calculation and the previous "fully Eulerian" calculation. The inlet and outlet flowrates in Fig. 3 still exhibit a numerical modulation. However, it appears to the author that the numerical modulation of the flowrates is less pronounced in Fig. 3 than in Fig. 1. The use of Eq. (1) lengthens the time interval between boiling initiation and

inlet flow reversal and retards the void propagation, reflecting the fact that the use of Eq. (1) results in generally lower effective two-phase pressure sources being applied to the upper and lower liquid slugs. When the interpolation procedure is employed, BIFLO predicts that inlet flow reversal occurs 0.57 s after boiling inception (as compared with 0.40 s for the previous fully Eulerian calculation). This figure is in excellent agreement with the SAS calculation which calculated that inlet flow reversal follows the onset of boiling by 0.55 s. However, as noted earlier, numerous differences exist between the boiling models in the two codes and the excellent agreement obtained here could be fortuitous.

Fig. 4. Two-Phase/Single Phase Interphases Calculated with BIFLO.

Our calculations with BIFLO suggest that, for the type of transient that we have considered (i.e. a rapid flow coastdown) and a numerical grid cell size similar to the one that we have assumed (which was essentially taken from SAS and is representative of the degree of spatial detail which must be assumed in whole-core accident calculations), Eulerian numerical effects can

significantly affect the calculation of sodium boiling and voiding phenomena using an Eulerian methodology. It will be interesting to see if a similar behavior due to Eulerian effects is obtained with other Eulerian codes (e.g., COMMIX-2 and THERMIT) for the calculation of sodium boiling and voiding phenomena.

Calculations with the initial BIFLO version indicate that it runs too slowly following the onset of boiling for whole-core accident analysis applications within the SAS4A framework. It is believed that an improvement in running time of about a factor of ten is required. However, the current running times are not a hindrance to performing calculations for a single bundle. The running speeds are currently limited by the two-phase portions of the code which must be solved with time steps limited by a Courant stability restriction based on the two-phase material velocities (i.e. $\frac{U \delta t}{\delta z} < 1$) due to an explicit formulation of convection within the two-phase Eulerian methodology. Thus, while the current methodology is relatively efficient on a computation per time step basis, too many time steps are required to calculate two-phase conditions over the time scales of several seconds or longer anticipated for accident analysis applications.

If the convective terms in the two-phase finite difference equations were formulated implicitly with a strong cell to cell coupling, then time steps exceeding the basic stability restriction could be used. For example, before boiling initiation, the single-phase portions of BIFLO (which possess such a strong implicit cell to cell coupling) are routinely run using time steps exceeding the Courant limit by an order of magnitude or more. Alternate numerical fluid dynamics methodologies for the calculation of two-phase flow permitting the use of time steps greatly exceeding the Courant limit are being reviewed. For example, the SETS method⁵ developed within the TRAC program at the Los Alamos Scientific Laboratory appears to have the potential of meeting our requirements for fast running times. Other candidate schemes (e.g., Refs. 6-7) will also be examined for possible use in BIFLO.

Benchmarking activities and computational studies will continue using the initial BIFLO version. If on the basis of studies with the initial BIFLO version, the BIFLO modeling approach is judged to be suitable for whole-core accident calculations and it appears that our requirements for running time can be achieved, then a faster running version of BIFLO will be developed for incorporation into the SAS4A framework.

B. BIFLO Benchmarking Activities (P. L. Garner)

A number of calculations have been performed for the intercomparison of computer codes and experiments for a 19-pin bundle. The experimental data were obtained in the THORS facility at Oak Ridge National Laboratory (ORNL). The 19 pins are electrically heated and are cooled by flowing sodium. The pin and wire wrap dimensions and power profiles simulate those of pins in the FFTF. The data records for the steady-state tests and selected transient tests are in References 8 and 9, respectively.

A series of calculations has been performed for comparison with Test-23/Run-101 in order to investigate parametric mixing models within several computer codes. Test-23/Run-101 was a steady state test in which power was

being generated only in the central pin (15 kW). The mass flow was 0.46 kg/s ($= 1.45$ m/s: inlet velocity), the inlet temperature was 652 K and the bundle-average temperature rise was 24 K. The data from the thermocouples at the end of the heated length are summarized in the first section of Table I in normalized form. Coolant Ring 1 is between the central pin and the first ring of pins; Coolant Ring 2 is between the first and second ring of pins; Coolant Ring 3 is between the second ring of pins and the duct wall. The two numbers for each region are the minimum and maximum of the readings obtained from thermocouples in the wire wrap, on the interior surface of the cladding, and (for Ring 3) in the duct wall.

Calculations have been performed with three computer codes varying the parameters which affect mixing between coolant regions. The calculations performed with BIFLO used three radial coolant regions and varied CSWEEP which is a coefficient on the forced-diversion crossflow due to the wire wrap; the general form of this modeling was taken from Reference 10 which recommends a coefficient corresponding to CSWEEP=0.023. The BIFLO calculations also included thermal conduction between coolant regions. The results of these calculations are shown in the second section of Table I.

Calculations performed with the COMMIX-1A code¹¹ utilized the quarter-pin partitioning geometry (92 nodes in an axial plane) and utilized the smeared and forced-diversion wire wrap modeling options. The base coefficients in the forced-diversion wire wrap model were CWIREI=0.1 and CWIREO=0.08; the (multiplicative) coefficients CWIREX, CWIREY, and CWIREZ were varied parametrically. The results of these calculations have been averaged for each coolant region and are shown in the third section of Table I.

Calculations performed using the COBRA-IV-I code¹² utilized 36 coolant cells in an axial plane and were used to investigate three parameters affecting mixing. The parameter DUR is the coefficient multiplying the forced diversion of coolant by the wire wrap; our calculations have shown the results are insensitive to the magnitude of DUR for $0 < \text{DUR} < 1.0$. The coefficient ABETA affects the amount of turbulent mixing between channels (i.e. energy transfer with no net mass transfer). A value of 0.02 was recommended¹³ for FFTF type conditions. The coefficient GK multiplies the thermal conduction between channels. The results of these calculations have been averaged for each coolant region and are shown in the fourth section of Table I.

The results shown in Table I indicate that the three calculation models produce very similar results under the following conditions: BIFLO with CSWEEP=0.023, COMMIX-1A with CWIREX/Y/Z=0.5, and COBRA-IV-I with DUR > 0, ABETA=0.02, and GK=1. A comparison of results calculated using these parameter values with the experiment indicates a somewhat lesser degree of mixing is present in the experiment than is being modeled in the calculations. Further calculations are being performed to examine mixing in other tests performed in THORS Bundle-6A (e.g., an off-center pin heated and all pins heated for different power and flow combinations).

The detailed data records have been obtained¹⁴ on magnetic tape for three transient tests performed in THORS Bundle-6A: Test-55A/Run-101, Test-72B/Run-101, and Test-73E/Run-102A. Two FORTRAN programs for plotting the test data from the magnetic tape were also obtained from ORNL and made operational

at ANL. The data of Test-73E/Run-102A are being analyzed first with the several calculational models (e.g., BIFLO and COMMIX). The early calculations using current estimates for mixing within the bundle are typically resulting

TABLE I. Normalized^a Temperature at End of Heated Length for THORS-6A/Test-23/Run-101 Experiment and Several Calculational Models

	Coolant Ring 1	Coolant Ring 2	Coolant Ring 3
Experiment	2.08-3.33	0.71-1.38	0.38-0.92 (duct = 0.46-0.58)
<u>BIFLO</u>			
CSWEEP=0.0001	4.08	0.69	0.22
CSWEEP=0.01	2.75	0.90	0.48
CSWEEP=0.023	2.03	0.96	0.67
<u>COMMIX-1A</u>			
smear wrap	3.15	0.88	0.28
CWIREX/Y/Z=0.001	3.17	0.89	0.28
CWIREX/Y/Z=0.5	2.06	0.98	0.58
CWIREX/Y/Z=1.0	1.66	0.99	0.74
<u>COBRA-IV-I</u>			
<u>DUR</u>	<u>ABETA</u>	<u>GK</u>	
smear	0	0	6.70 0.0 0.0
0.18	0	0	5.46 0.42 0.02
0.18	0.01	0	3.58 0.86 0.22
0.18	0.02	0	2.74 0.96 0.42
0.18	0.01	1	2.75 0.95 0.43
0.18	0.02	1	2.26 0.98 0.56

^aNormalized temperature is the designated temperature minus the inlet temperature divided by the bundle average temperature rise.

Test Conditions: Power = 15 kW (central pin only), mass flow = 0.46 kg/s, inlet temperature = 652 K, bundle average temperature rise = 24 K.

in temperatures in the central portion of the bundle which are too low relative to the experiment results at steady state. The initial transient calculations have indicated that the thermal characteristics of the fuel pin simulators and the region outside of the duct wall are very important and that better descriptions of these regions are needed in the calculational models than were required for steady-state analyses.

C. Incorporation of the NEFIG Transient Gas Release Model into the SAS3D/EPIC Code (Kalimullah, H. H. Hummel and P. A. Pizzica)

The transient fission gas release routine FFRATE in the SAS3D/EPIC Code has been recoded to include as a third option a modified version of the NEFIG model^{15,16}. In this option, the calculation of the fractional fission gas release rate dF/dt , expressed as a fraction of total (intragranular and intergranular gases) retained fission gas, is divided into the following five fuel temperature (T , in K) ranges:

- (1) $T < T_{b1}$, where T_{b1} is a model parameter currently set at 1773 K. Over this fuel temperature range, no fission gas is assumed to be released.

$$\dot{F} = 0 \quad (2)$$

- (2) $T_{b1} < T < T_{b2}$, where T_{b2} is a model parameter currently set at 2173 K. Over this fuel temperature range, a user-specified amount of grain boundary fission gas (intergranular gas), expressed as a fraction f_b of the total retained fission gas at the beginning of the transient heating, is released linearly with temperature.

$$\dot{F} = \frac{f_b \dot{T}}{(T_{b2} - T_{b1})} \quad (3)$$

- (3) $T_{b2} < T < T_m$ (solidus or when the fuel first reaches melting temperature), where T_m is fuel melting temperature, the fuel solidus temperature and liquidus temperature being equal in the SAS3D code. This temperature range terminates when the fuel first reaches T_m and at that time the fuel melt fraction will have a small or zero value V_{fm}' . Over this temperature range, the NEFIG model^{15,16} described below is used to compute the time-dependent variable $h(t)$ where $(d_g - h)$ is the distance moved by the fission gas bubbles and d_g is the grain diameter. The time-dependent variables of the model are initialized when the fuel temperature is greater than T_{b2} for the first time and at this time only the intragranular fission gas is contained in the fuel. The fractional release rate $\dot{F}$, expressed as a fraction of total (intragranular and intergranular) retained fission gas at the beginning of the transient heating, is given in terms of the variable $h(t)$ by the equation,

$$\dot{F} = - \frac{3h\dot{h}}{2d_g^3} (2d_g - h)(1 - f_b). \quad (4)$$

The quantity on the right hand side of Eq. (4) besides the factor $(1 - f_b)$ is the fractional release rate of the intragranular fission gas obtained using the NEFIG model.

- (4) $T = T_m$, $0 < V_{fm} < 1$, where $V_{fm}(t)$ is the time-dependent fuel melt fraction, i.e., this temperature range extends from the fuel solidus (or when the fuel first reaches melting temperature) to fuel liquidus (or when the fuel temperature is greater than T_m for the first time). Over this part of the

transient, the remaining intragranular fission gas is released linearly with the fuel melt fraction.

$$\dot{F} = \frac{(1 - F'_m)}{(1 - V'_{fm})} \frac{dV_{fm}}{dt}, \quad (5)$$

where F'_m is the fission gas release fraction at the end of the third temperature range when the melt fraction is V'_{fm} .

- (5) $T > T_m$. All the fission gas should be released when the fuel reaches the liquidus. However, the release fraction may remain slightly less than 1.0 at the liquidus due to numerical integration. To take account of this possible discrepancy, and for the sake of defining the release rate at all fuel temperatures, the fractional release rate $\dot{F}$ is set equal to a very large number (1.0×10^{20}) over this temperature range.

The NEFIG Model: The model is described in some detail in References 15 and 16. The main purpose of the present description is to give the values of all the parameters of the model and the initial values of the time-dependent variables as coded in the SAS3D-EPIC Code, after giving the equations of the model.

In the NEFIG model, the fission gas bubble population is represented by a single bubble size. The five time-dependent variables, (1) the distance moved by the bubbles (d_g -h), d_g being the grain diameter, (2) the dissolved gas atom concentration c_g , (3) the bubble concentration c_b , (4) the number of gas atoms in a bubble n_b , and (5) the bubble radius r_b , are given by the following five coupled differential equations:

$$\dot{h} = - \frac{2\lambda D_s Q^* \beta |VT|}{r_b kT^2}, \quad (6)$$

$$\dot{c}_g = - \pi r_b c_g c_b [r_b v_b + 4(D_g + D_b)], \quad (7)$$

$$\dot{c}_b = - r_b c_b^2 (\alpha_c r_b v_b + 8\pi D_b), \quad (8)$$

$$\dot{n}_b = - (\dot{c}_g + n_b \dot{c}_b) / c_b, \quad (9)$$

and

$$\dot{r}_b = \frac{D_u P_{ex} \Omega}{r_b kT(1 + \sqrt{\pi} r_b c_b d_g / 3)} - \frac{r_b \dot{c}_b}{3c_b} - \frac{v \Omega \dot{c}_g}{4\pi r_b^2 c_b}, \quad (10)$$

where VT is the mean fuel temperature gradient; v_b is the bubble velocity and equals $-h$; D_b is the bubble diffusion coefficient; P_{ex} is the effective bubble excess pressure; and the physical meaning of all the other parameters are given in Table II with their values as coded in the SAS3D-EPIC code.

The bubble diffusion coefficient D_b is obtained from the equation,

$$D_b = \frac{3\lambda\Omega D_s}{2\pi r_b^4} \quad (11)$$

The effective bubble excess pressure P_{ex} is given by

$$P_{ex} = P_g - P_o - \frac{2\gamma}{r_b}, \quad (12)$$

where P_g is the internal gas pressure calculated using the Van der Waals equation,

$$P_g = n_b kT / (4\pi r_b^3/3 - bn_b) - 27 P_c \left(\frac{bn_b}{4\pi r_b^3/3} \right)^2 \quad (13)$$

TABLE II. Parameters and Physical Properties Used in the NEFIG Model as Coded in the SAS3D-EPIC Code.

(Quantities labelled "C" are built into the code; those labelled "D" are input by the user, with the default values given.)

(1) Effective surface thickness for surface diffusion (λ)	3.4×10^{-8} cm	C
(2) Surface diffusion coefficient (D_s)	$20000 \exp(-45280/T)$ cm ² /sec	D
(3) Heat of transport for surface diffusion (Q_s^*)	0.7×10^{-11} erg/molecule	D
(4) Coefficient to account for local perturbation in temperature caused by the bubble (β)	1.5	C
(5) Boltzmann constant (k)	1.38×10^{-16} erg/K-molecule	C
(6) Gas atom diffusion coefficient (D_g)	$0.039 \exp(-45280/T)$ cm ² /sec	D
(7) Coalescence parameter (α_c)	2π	D
(8) Cation self-diffusion coefficient (D_u)	$2 \exp(-55650/T)$ cm ² /sec	D
(9) Atomic volume (Ω)	40.9×10^{-24} cm ³	C
(10) Number of atomic volumes associated with each gas atom in solution (ν)	3	C
(11) External restraint pressure (P_o)	50×10^6 dynes/cm ²	C
(12) Surface energy (γ)	1000 erg/cm ²	C
(13) Van der Waals constant for fission gas, Xe/Kr=0.87/0.13 (b) (Reference 17)	63.55×10^{-24} cm ³ /molecule	C
(14) Critical pressure for fission gas (P_c)	57.9×10^6 dynes/cm ²	C

The physical meaning and values of the parameters P_o , γ , b and P_c are given in Table II.

The fraction of the intragranular fission gas released, $f(t)$, at any time t is given by

$$f(t) = 1 - \frac{h^2(3d_g - h)}{2d_g^3}, \quad (14)$$

and the corresponding release rate is obtained by differentiating Eq. (14),

$$\dot{f} = -\frac{3hh}{2d_g^3} (2d_g - h). \quad (15)$$

The following initial conditions are used for the five time-dependent variables h , c_g , c_b , n_b , and r_b in order to integrate the first-order differential equations (6-10):

$$\left. \begin{aligned} h(0) &= d_g, \\ c_b(0) &= 2 \times 10^{18} \text{ bubbles/cm}^3, \\ n_b(0) &= 2, \\ c_g(0) &= (1 - f_b)c_o - n_b(0)c_b(0) \text{ atoms/cm}^3, \\ r_b(0) &= \left[\frac{3}{4\pi} v n_b(0)\Omega \right]^{1/3} \end{aligned} \right\} \quad (16)$$

where c_o is the total retained fission gas (intragranular and intergranular) at the beginning of the transient heating; and $r_b(0)$ has been computed from a bubble volume of $v n_b(0)\Omega$.

D. WAC 0.10 \$/s TOP (P. A. Pizzica and H. H. Hummel)

The following are the results of the SAS3D/EPIC calculation of the 0.1 \$/s transient overpower (TOP) accident for the irradiated core of the "Europe" reactor¹⁸, which is the current assignment for the Whole-Core Accident Codes (WAC) study group.

Results for the Pre-Failure Phase of the Transient

The input reactivity ramp rate during this transient was assumed to be 0.1 \$/s. In spite of the fact that the Doppler reactivity ramp rate approximately canceled the input rate, a small net positive ramp rate resulted because of the reduction of the density of the sodium. The new ramp rate averaged about 0.02 \$/s. Fuel melting in channel 2, the highest power irradiated channel, began about 7 s into the transient. The reactor power rose slowly and steadily until it was about 3.8 times nominal at the time of pin failure in channel 2. Due to the slow character of the transient, boiling was able to commence in channel 1 at 13.914 s, only about 0.04 s before pin failure in channel 2. This boiling had almost no effect on the calculation, however.

Pin Failure Criteria

In the irradiated pins undergoing the transient, it seems very likely that the mechanism of cladding failure would be fission gas pressure loading of the cladding. Also, it seems reasonable to assume that there will be at least some and probably much redistribution of fission gas causing equilization of fission gas pressure over much of the fuel pin, especially since the molten fuel cavity extends over the whole length of the core. There seems to be more than adequate time during this transient to allow equilization. The SAS3D/EPIC calculation assumes that the gas pressure is equilibrated radially as well as axially in the fuel pin.

It would seem, therefore, that failure would occur at the highest node of the molten fuel cavity (i.e. at the top of the core) when the equilibrated cavity pressure caused a hoop stress in the cladding in excess of the failure strength of this highest node, assuming this latter quantity to be known (presumably as a function of temperature). The problem with this simple approach is that there might be very little molten fuel at the point where failure would be predicted, thus making the escape of molten fuel at that location at that time unlikely.

This leads to two options. Either the release of molten fuel is assumed to occur at the highest cavity node but is delayed until a sufficiently high melt fraction is attained there, or release of molten fuel is assumed to occur at a lower position before the highest cavity node has reached the required melt fraction. There are reasonable arguments to be made for both these options for the general case. However, in this particular transient relatively high fuel melt fractions were attained at locations below the top of the cavity where cladding strength could be exceeded by the hoop stress caused by the average cavity pressure. Although the cooler cladding would have inhibited failure below a certain level, fuel melt fractions probably high enough to allow release of molten fuel occurred along a length of the pin from the center up to 80% or even 90% of core height with a variation in cladding temperature of 100 K or more.

Therefore, it seems reasonable to assume a failure location for this exercise for irradiated pins well above the center of the core but still far enough below the top of the molten fuel cavity to allow at least a 0.3-0.4 fuel melt fraction at the failure location. This implies that the point of molten fuel release is between 70 and 90% of core height with approximately a 0.3-0.4 minimum melt fraction at that point. The criterion of 0.5 melt fraction at 80% of core height is probably an adequate estimate of failure conditions. However, the 0.5 melt fraction (0.71 radius fraction) may seem on the high end; but it is quite possible that fission gas pressures sufficient for failure would not be reached until the melt fraction of 0.5 was approached anyway.

Conditions at First Pin Failure in Channel 2

TABLE III. ROEC Results at 0.5 Fuel Melt Fraction at 80% Core Height

	Units	
Time	s	13.9539
Normalized Power	-	3.785
Period	s	7.57
Net Reactivity + Ramp	\$/s	0.319, 0.027
Doppler Reactivity + Ramp	\$/s	-1.282, -0.115
Coolant Reactivity + Ramp	\$/s	0.205, 0.042
Total Energy Released	GJ	53.777
Channel 2 Data:		
Cavity Pressure	MPa	30.5
Melt Fraction	-	0.5
Cladding Temperature	K	1202
Cladding Stress	MPa	218

The assumed criterion for failure of irradiated pins, then, was 0.5 melt fraction at 80% core height. Since channel 2 was the highest power irradiated channel, it failed first at 13.954 s into the transient. Net reactivity at this time was 0.32 \$. The maximum fuel melt fraction in channel 2 was 0.64. Table III shows conditions at failure time.

The fission gas pressure in the pin (assumed to be equilibrated in all the void space along the whole core length of the fuel pin) was 30.5 MPa. A

cladding failure one node long (74 mm) was assumed. This seemed appropriate given the temperature of the cladding (1202 K). The homogenized temperature of the molten fuel at the failure location was 3679 K; the sodium temperature at that location was 1119 K. The peak homogenized molten fuel temperature in any axial cell in the cavity was 3932 K. A considerable amount of molten fuel expulsion was to be expected since the 0.5 melt fraction would not cause a significant orificing of the flow of molten fuel up to the failure location and out into the channel and also since relatively high pin pressures prevailed. The constant fragmented particle size assumed for the EPIC modeling was 100 μm .

Results for the Post-Failure Phase of the Transient

As noted above, conditions were quite conducive to the ejection of a considerable amount of fuel and about 57 g/pin (about 21% of the core fuel) was ejected by 45 ms after pin failure at a fairly steady rate. By 45 ms the pin cavity pressure had fallen to a level (about 2 MPa) equal to the ambient coolant channel pressure from sodium vapor. Ejection from this time onward was effectively cut off since the cladding temperature at the rupture point had risen from its prefailure value of 1202 K to 1595 K and thus there was no possibility for further cooling of sodium at this temperature via condensation on the cladding. There is currently no energy treatment for pure sodium vapor in EPIC and the vapor temperature did not fall below the cladding temperature.

Since all the molten fuel ejected into the coolant channel was assumed to be fragmented into 100 μm radius particles, a considerable FCI resulted in the first 10 ms or so after pin failure. Sodium temperatures in the vicinity of the rupture almost doubled to about 2200 K by 6 ms after failure. (See Figure 5.) Then the increasing separation of liquid sodium and molten fuel cut off the efficient heat transfer between them leading to lower average sodium temperatures in the interaction zone. The combination of high fission gas pressure initially and later sodium vapor pressures led to complete expulsion of the upper sodium liquid slug from the subassembly by 11 ms after pin failure and the lower slug interface reached the bottom of the core about 31 ms after failure. (See Figure 6.) The general leveling off of the fuel reactivity between 7 and 15 ms which led to a net reactivity rise with the sodium feedback was caused by some fuel moving downwards from the failure which temporarily canceled the reactivity effect of the upward sweepout of fuel. (See Figure 7.)

The voiding of the lower sodium slug provided a significant reactivity addition which exceeded the negative fuel motion reactivity addition until about 20 ms after pin failure, when the net reactivity followed the fuel motion feedback downwards, except for a slight aberration between 40 and 50 ms caused by fuel reentering the top of the core driven by an FCI above the core. This reentering fuel might only be indicative of the lack of modeling of fuel plateout above the core rather than a realistic prediction of events. The last upturn in the fuel reactivity after 85 ms was caused by an FCI below core driving some fuel upwards. Figure 8 shows the post-pin failure power history. The maximum power level attained was a little more than 7 times nominal power.

The calculation could not be continued further profitably since, in order to do so, a heat conduction model in the solid fuel in the pin as well as a plateout model for molten fuel in the channel would be needed. These models

Fig. 5. Average Interaction Zone Fuel and Sodium Temperatures vs. Time After Pin Failure in Channel 2.

Fig. 6. Interaction Zone Boundaries vs. Time After Pin Failure in Channel 2.

Fig. 7. Reactivity vs. Time After Pin Failure in Channel 2.

Fig. 8. Relative Power vs. Time After Pin Failure in Channel 2.

are currently not included in EPIC. However, despite the lack of these modeling features, there is a clear indication of a significant amount of fuel sweepout and intra-pin fuel motion towards the failure in the calculation. This fuel motion more than makes up for the positive feedback from sodium voiding. In order to postulate a reactivity increase resulting from further fuel motion, there would have to be coherent reentry of a large portion of the fuel already swept out (and probably deposited on cladding in lower worth regions) and this possibility seems very remote. This prediction does neglect, however, the possibility of a positive reactivity addition from cladding motion.

E. Consultations and Cooperative Studies

WAC Meeting

H. Hummel and P. Pizzica attended the WAC Comparative Studies Group meeting at Ispra, Italy on June 17 and 18.

Results for the 0.10 \$/s TOP for the irradiated core of the "Europe" reactor were presented. This exercise was discussed at the meeting as well as the form of a report on the work to be published in the future. It was agreed to begin consideration of the LOF case for the irradiated core at the next meeting of the WAC group.

UK/NRC Bilateral Program

H. Hummel and P. Pizzica were at the UKAEA at Risley on June 13 and 16.

The report on the work completed to date under the bilateral studies program with the UK was completed except for editorial changes. These will be finished in the near future and the final report should be distributed within several months time. One last LOF study was completed during this quarter which will be included in the report. The power-to-flow ratio was changed in the various reactor subassemblies based upon new data that more accurately reflected design conditions. The new data did not alter any of the general conclusions arrived at previously in earlier studies. Some clad motion occurred because of greater incoherence in channel power-to-flow ratios. However, the LOF-TOP potential was not significantly different.

The future work in this program, which in the next year or two will be focused on individual phenomena such as sodium boiling and fuel pin failure, was also discussed, based on a draft proposal submitted by us. The UK will reply with a more detailed proposal covering the same points.

SAS3D/EPIC and EPIC Stand-Alone

The codes were prepared for export to users by the addition of some final minor modeling changes and by the addition of comment cards to make the code more understandable.

EPIC User's Guide

The User's Guide was completed and printed and distributed.

Other Visits and Meetings

P. L. Garner attended the Ad Hoc Workshop on Severe Core Damage Studies in LWR's at NRC in Silver Spring, Maryland on April 17, 1980.

P. L. Garner visited Oak Ridge National Laboratory on June 3, 1980 to obtain data from several transient sodium boiling experiments performed in the THORS Facility and to discuss data interpretation and experiment modeling with P. W. Garrison, W. R. Nelson, and G. A. Klein.

J. J. Sienicki served as a session chairman at the ANS National Meeting at Las Vegas, Nevada, June 1980.

Publications

P. A. Pizzica, P. L. Garner and P. B. Abramson, "A User's Guide to EPIC, a Computer Program to Calculate the Motion of Fuel and Coolant Subsequent to Pin Failure in an LMFBR," NUREG/CR-1504, ANL-80-47.

II. THREE-DIMENSIONAL CODE DEVELOPMENT FOR CORE THERMAL-HYDRAULIC ANALYSIS OF LMFBR ACCIDENTS UNDER NATURAL CONVECTION CONDITIONS

A2045

A. Introduction

The objective of this program is to develop computer programs (COMMIX and BODYFIT) which can be used for either single-phase or two-phase thermal-hydraulic analysis of reactor components under normal and off-normal operating conditions. The governing equations of conservation of mass, momentum, and energy are solved as a boundary value problem in space and initial value problem in time.

COMMIX is a three-dimensional, transient, compressible flow computer code for reactor thermal-hydraulic analysis. It is a component code and uses a porous medium formulation. The concept of volume porosity, surface permeability, and distributed resistance and heat source (or sink) is employed in the COMMIX Code for quasi-continuum (or rod-bundle) thermal-hydraulic analysis. It provides a greater range of applicability and an improved accuracy than subchannel analysis. By setting volume porosity and surface permeability equal to unity, and resistance equal to zero, the COMMIX code can equally handle continuum problems (reactor inlet or outlet plenum, etc.).

BODYFIT is a three-dimensional, transient, compressible flow computer code for reactor rod bundle thermal-hydraulic analysis. This is also a component code, and it uses a boundary-fitted coordinate transformation. The complex rod bundle geometry is transformed into either rectangular or cylindrical coordinates with uniform mesh. Thus, the physical boundaries, including each rod, coincide with computational grids. This allows the Navier-Stokes equations, together with the boundary conditions, to be represented accurately in the finite-difference formulation. Thus, the region in the immediate vicinity of solid surfaces, which is generally dominant in determining the character of the flow, can be accurately resolved.

During this quarter all efforts were devoted to the development of COMMIX-2 (3-D, two-phase) computer code and BODYFIT-1 code.

B. COMMIX-2, Two-Phase Code Development (J. G. Bartzis, H. M. Domanus, J. L. Krazinski, C. C. Miao, W. T. Sha and V. L. Shah)

During this quarter, the following three problems were analyzed using the line by line solution procedure with SIMPLER¹⁹ formulations incorporated in COMMIX-2.

1. Two-Phase Flow in a Heated Duct

Problem Description

The problem considered is the case of a subcooled liquid (H_2O) entering a rectangular duct and beginning to boil as it flows through this uniformly

heated duct. The schematic diagram is shown in Fig. 9. The description of the test section geometries is given in Table IV.

TABLE IV. Description of Experimental Conditions and Test Sections

	Pressure MPa	V-inlet m/sec	Heat Flux kW/m ²	Rectangular Test Section		
				Cross Section m x m	Length m	Hydraulic Diameter
Egen et al.	13.79	1.07-1.43	189-1576	.024x.0026	0.686	4.75×10^{-3}
St. Pierre	1.38-5.52	0.77-1.15	71-287	.011x.044	1.257	1.78×10^{-2}

The steady state flow problem as described above was selected for the following three reasons:

- (i) To demonstrate the capability of the extended SIMPLER solution procedure to analyze two-phase flow problems.
- (ii) To determine a model for the momentum exchange coefficient which is suitable over a wide range of flow conditions.

and

- (iii) Experimental measurements were available with which we can compare the predictions of COMMIX-2.

Momentum Exchange Coefficient

When we used the proposed model of Autruffe, et al.²⁰ for the momentum exchange coefficient, the results showed a correct trend, but did not agree with the measurements. This was expected as a correlation coefficient of Autruffe et al. was determined from the sodium measurements. Our parametric analyses (Figs. 10 and 11) with various values of correlation coefficient showed that

- (i) The interfacial momentum exchange term is very important and the results are very sensitive to the model used.
- (ii) The model of Autruffe et al. predicted results that are very close to homogeneous model predictions.

and

- (iii) The correlation coefficient equal to 4.31×10^{-3} predicted results that are in close agreement with the measurements of St. Pierre and Egen et al.

The modified model for the momentum exchange coefficient that was used in the COMMIX-2 is

$$K = 4.31 \times 10^{-3} \frac{1}{2} \frac{\rho_g |u_g - u_l|}{D_h} \{(1 - \theta_g)[1 + 75(1 - \theta_g)]\} \quad (17)$$

Here, $K(\text{kg/m}^3 \cdot \text{sec})$ is the momentum exchange coefficient; ρ , u , and θ are the density, velocity and void fraction, respectively; D_h is the hydraulic diameter and the subscripts g and l are the gaseous phase and liquid phase respectively.

Results

The comparison of COMMIX-2 results has been made with

- a) Ten sets of data of St. Peirre²¹ with pressure ranging from 1.379 MPa (200 psia) to 5.516 Mpa (800 psia); To illustrate the kind of agreement obtained, comparisons of only two test runs are presented here. The void fraction comparisons are presented in Figs. 12-13 and the velocity comparisons are presented in Figs. 14-15.
- b) Thirteen sets of data of Egen et al.²² at 13.79 MPa (2000 psia); The void fraction comparisons for two of the thirteen test runs are presented here in Figs. 16-17.

The results of the data comparison indicate that:

- a) The solution scheme, the extension of SIMPLER to two-phase flow, used in COMMIX-2 is capable of analyzing two-phase problems.
- b) The model for momentum exchange coefficient, Eq. (17), is applicable over a wide range of flow conditions.
- c) Some disagreement in the initial stage of boiling is due to subcooled boiling. Efforts will be made to incorporate the subcooled boiling model in the code.
- d) Two equation model is necessary to describe the conservation equations, as most two-phase flow conditions are nonhomogeneous.

2. NRC Standard Problem No. 1

Problem Description

In the standard problem No. 1,²³ liquid water in a closed pipe was initially at temperature $T = 467^\circ\text{F}$ ($= 242^\circ\text{C}$) and high pressure $p = 1000$ psig ($= 7 \times 10^6$ Pa). The diameter and length of the pipe was 2.88 in (0.0732m) and 13.44 ft (4.097m) respectively. At time $t = 0$, the glass disc at one end of the pipe was ruptured to atmospheric pressure, the other end of the pipe remaining closed. The experiment involved flash boiling due to sudden decrease in pressure.

During the transient, the pressures were measured at seven locations along the length of the pipe (GS-1 to GS-7). In addition, the void fraction and temperature measurements were made at the location GS-5. There were no velocity measurements. The schematic layout is shown in Fig. 18.

Results

Figures 19a and 19b show the late-time pressure history at Stations GS-1 and GS-5. Station 1 is at $z = 0.55$ ft (0.168m) from the open end of the pipe and Station 5 is at $z = 8.62$ ft (2.627m) from the open end. In reality, the pressure drops sharply from its initial value ($p = 7 \times 10^6$ Pa) at $t = 0$ sec to $p = 2.5 \times 10^6$ Pa in 1 ms. This large drop is not shown in Fig. 19.

The early-time (1-20ms) history of pressures at these same two recording stations is shown in Figs. 20 and 21. As can be seen in Fig. 21, the predictions do not closely follow the experimental data for the period 0-6 ms. This is due to the relatively large time step used ($1.E-3$ sec). Runs made with a smaller time step ($1.E-4$ sec) showed better agreement in this range.

The comparison between the measured and predicted void fractions is presented in Fig. 22. This analysis was made without considering condensation. However, the predicted results do not differ appreciably from those of Rivard and Torrey²⁴ even though a condensation model was included in their analysis.

Figure 23 shows the predicted vapor and liquid velocities. No experimental measurements of the velocities are presented in the report by Edward and O'Brien. The results in Fig. 23 are sensitive to the wall friction and interphase friction models used in the computer code. For these predictions, we had used the same wall friction and interphase friction models as those used by Rivard and Torrey.

The comparison between the measured and predicted temperature at GS-5 is shown in Fig. 24. It is not clear from the report of Edward and O'Brien whether the temperature measurements were for liquid or vapor. Our comparison is based on the assumption that it was the liquid temperature that was measured. Rivard and Torrey used a high value of interphase heat transfer coefficient. Consequently, their liquid and vapor temperatures were very nearly equal. Also, they observed that changing the value of interphase heat transfer coefficient by a factor of four did not make any appreciable difference in their results for pressure transients.

3. German Seven-Pin Hexagonal Fuel Assembly²⁵

Problem Description

In this test, liquid sodium having a temperature of 553°C enters the hexagonal test section, with a uniform velocity of 2.15 m/sec. The test section has seven electrically heated rods arranged in a hexagonal array, as shown in Fig. 25. At time $t = 0$, the inlet velocity begins the transient wherein the flow rate is decreased to 30% of its initial value in 10 seconds (see Fig. 26).

Results

The variations of the exit coolant temperature and the central cladding surface temperature at the end of the heated section are compared with the experimental measurements in Fig. 27 and 28. Radial distributions of the void fraction at the exit plane and at the end of the heated section are presented in Figs. 29 and 30 respectively. The axial distribution of the void fraction along the central axis is shown in Fig. 31. We can see from these figures that the COMMIX-2 predictions are in close agreement with the measurements.

Other Efforts

In addition to the analysis of the above three problems, the efforts were also devoted toward formulating, incorporating and implementing the "Thermal Structure" model in COMMIX-2.

In regard to the development of the Analytic Rebalance solution technique, efforts were devoted to achieve the strict mass convergences of both liquid and vapor phases separately in the region of high boiling activity. We have been successful in obtaining the convergence of the mixture mass and in stabilizing the convergence process of individual phases. Efforts are continued to obtain a complete transient solution of the NRC Standard Problem No. 1 and to compare the results with the experimental measurements.

C. BODYFIT-1, Single-Phase Code Development (B. C-J. Chen and W. T. Sha)

In the area of BODYFIT code development, the formation for the pressure correction used in the 2-D geometry has been generalized to the 3-D geometry. The generalized formula for the pressure correction can be derived briefly as follows:

Let (x, y, z) be the coordinates in the transformed space. The Jacobian of the transformation can be written as:

$$J = \frac{\partial(x, y, z)}{\partial(\xi, \eta, \zeta)} = \begin{vmatrix} \frac{\partial x}{\partial \xi} & \frac{\partial x}{\partial \eta} & \frac{\partial x}{\partial \zeta} \\ \frac{\partial y}{\partial \xi} & \frac{\partial y}{\partial \eta} & \frac{\partial y}{\partial \zeta} \\ \frac{\partial z}{\partial \xi} & \frac{\partial z}{\partial \eta} & \frac{\partial z}{\partial \zeta} \end{vmatrix}$$

The cofactors of the Jacobian are defined as C 's. For example, C_{23} is given as:

$$C_{23} = - \begin{vmatrix} \frac{\partial x}{\partial \xi} & \frac{\partial x}{\partial \eta} \\ \frac{\partial z}{\partial \xi} & \frac{\partial z}{\partial \eta} \end{vmatrix}$$

The approximated momentum equation and the continuity equation in the transformed space can be written as:

$$\Delta v_i = - \frac{\Delta t}{\rho J} (C_{i1} \Delta P_\xi + C_{i2} \Delta P_\eta + C_{i3} \Delta P_\zeta) \quad (18)$$

and

$$\Delta R = \frac{\rho}{J} (C_{i1} \Delta v_{i,\xi} + C_{i2} \Delta v_{i,\eta} + C_{i3} \Delta v_{i,\zeta}) \quad (19)$$

Substituting Eq. (18) into Eq. (19), one obtains

$$\Delta P = \frac{2J^2}{\Delta t \sum_{i,j} C_{ij} C_{ij}} \Delta R \quad (20)$$

This generalized pressure correction formula with a relaxation factor has been programmed into the code.

Also during this quarter, considerable amount of efforts were devoted to cleanup the code for the purposes of final documentation. Some of the improvements are:

- (i) Inclusion of comment statements inside the program to describe the calculations being performed;
- (ii) Simplification of the input procedure for the convenience of all users;
- (iii) Elimination of unnecessary output listings for clarity;
- (iv) Incorporation of default values for all the input variables; and,
- (v) Inclusion of options to bypass the momentum equation or energy equation and to permit constant property calculations in order to save computer time in certain applications.

Furthermore, the coordinate generation code, developed for the hexagonal geometry, has been extended to generate coordinates for square geometry. One program, therefore, can treat both geometries. An option has been incorporated which permits a user to select either of the two geometries. Figure 32 gives the calculational meshes for 16 pin square rod bundle obtained with this expanded computer program. The coordinate lines are satisfactorily orthogonal.

Also, we have completed the comparison between the BODYFIT predictions and the experimental data for the German 7-pin sodium boiling experiment. Figure 33 shows the geometry of the 7-pin bundle, both before and after the coordinate transformation. The flow parameters are also included in this figure. Figure 34 gives the axial and radial locations of the thermocouples. Figure 35 gives the flow history of the transient. These three figures have been reported previously in quarterly reports. They are included here for completeness. Figures 36 through 48 give the comparison between the code predictions and the experimental measurements. The agreements are satisfactory.

In the coming months, we will continue the cleaning up efforts to prepare the document of the code for preliminary release to the public. After release, we plan to set up a workshop for BODYFIT users. This will be to familiarize the users with our code and at the same time obtain their comments before the final release of the code.

Fig. 9. Schematic Layout of Two-Phase Flow in a Heated Duct.

TWO PHASE FLOW IN A HEATED DUCT

Fig. 10. Effect of Correlation Coefficient of the Momentum Exchange on the Void Fraction Distribution.

TWO PHASE FLOW IN A HEATED DUCT

Fig. 11. Effect of Correlation Coefficient of the Momentum Exchange on the Void Fraction Distribution.

TWO PHASE FLOW IN A HEATED DUCT

Fig. 12. Comparison of Liquid Vapor Velocity Distribution with the Measurements of St. Pierre.

TWO PHASE FLOW IN A HEATED DUCT

Fig. 13. Comparison of Liquid and Vapor Velocity Distribution with the Measurements of St. Pierre.

TWO PHASE FLOW IN A HEATED DUCT

Fig. 14. Comparison of the Void Fraction Distribution with the Measurement of St. Pierre.

TWO PHASE FLOW IN A HEATED DUCT

Fig. 15. Comparison of the Void Fraction Distribution with the Measurements of St. Pierre.

TWO PHASE FLOW IN A HEATED DUCT

Fig. 16. Comparison of the Void Fraction Distribution with the Measurements of Egen et. al.

TWO PHASE FLOW IN A HEATED DUCT

Fig. 17. Comparison of the Void Fraction Distribution with the Measurements of Egen et. al.

Fig. 18. General Arrangement of Blowdown Pipe.
(NRC Standard Problem #1)

Fig. 19a. Comparison Between Measured and Predicted Pressures.

Fig. 19b. Comparison Between Measured and Predicted Pressures.

Fig. 20. Comparison Between Measured and Predicted Pressures.

Fig. 21. Comparison Between Measured and Predicted Pressures.

Fig. 22. Comparison Between Measured and Predicted Void Fractions.

Fig. 23. Predicted Vapor and Liquid Exit Velocities.

Fig. 24. Comparison of Predicted Liquid Temperature with the Measured Temperature at GS-5.

Fig. 25.
(A) Axial and (B) Transverse
Partitioning of Model 7-Pin Bundle.

TEST 7-2/16, FLOW TRANSIENT

Fig. 26. Flow Rundown Transient for Test 7-2/16.

Fig. 27. Comparison of Exit Coolant Temperature with the Experimental Data.

Fig. 28. Comparison of the Central Cladding Surface Temperature at the End of the Heated Section with the Experimental Data.

Fig. 29. Radial Distribution of the Void Fraction at the Exit Plane.

Fig. 30. Radial Distribution of the Void Fraction at the End of the Heated Section.

Fig. 31. Axial Distribution of the Void Fraction Along the Central Axis.

Fig. 32. Computational Mesh for a 4×4 Square Rod Bundle.

RADIUS = 3.0 MM
 GAP BETWEEN PINS = 1.9 MM
 FLAT TO FLAT = 22 MM
 $V_M = 2.15 \text{ M/s}$
 $Re = 3.373 \times 10^4$
 HEAT FLUX = $.993 \times 10^6 \text{ WATTS/M}^2$

(a)

(b)

Fig. 33. (A) Physical Plane and Dimensions,
 (B) Transformed Plane. ANL Neg. No.
 116-79-192R1.

Fig. 34. (A) Axial and (B) Transverse Partitioning of Model 7-Pin Bundle.
ANL Neg. No. 116-80-139.

FLOW HISTORY

Fig. 35. Inlet Velocity vs. Time During Transient.

TC 3 TEMPERATURE

Fig. 36. Comparison of Temperature During Transient for Thermocouple TC 3.

TC 18 TEMPERATURE

Fig. 37. Comparison of Temperature During Transient for Thermocouple TC 18.

TC 19 TEMPERATURE

Fig. 38. Comparison of Temperature During Transient for Thermocouple TC 19.

TC 4 TEMPERATURE

Fig. 39. Comparison of Temperature During Transient for Thermocouple TC 4.

TC 5 TEMPERATURE

Fig. 40. Comparison of Temperature During Transient for Thermocouple TC 5.

TC 21 TEMPERATURE

Fig. 41. Comparison of Temperature During Transient for Thermocouple TC 21.

TC 22 TEMPERATURE

Fig. 42. Comparison of Temperature During Transient for Thermocouple TC 22.

TC 7 TEMPERATURE

Fig. 43. Comparison of Temperature During Transient for Thermocouple TC 7.

TC 8 TEMPERATURE

Fig. 44. Comparison of Temperature During Transient for Thermocouple TC 8.

TC 26 TEMPERATURE

Fig. 45. Comparison of Temperature During Transient for Thermocouple TC 26.

TC 27 TEMPERATURE

Fig. 46. Comparison of Temperature During Transient for Thermocouple TC 27.

TC 30 TEMPERATURE

Fig. 47. Comparison of Temperature During Transient for Thermocouple TC 30.

TC 9 TEMPERATURE

Fig. 48. Comparison of Temperature During Transient for Thermocouple TC 9.

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